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CONTRIBUTION TO THE SELECTION OF THE PARAMETERS OF THE THERMODYNAMIC CYCLE IN DOUBLE FLOW TURBOJETS

Mario Albin and Massimo Feola

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16. Abstract

In the most recent developments of double flow turbojet propulsion, there has been a large increase in secondary flow capacity, in order to obtain further increases in propulsive efficiency. The two most characteristic elements of this are the ratios of compression and of bypass of the fan. Studies are presented here which show that if the bypass ratio improves global thermopropulsive efficiency, at the same time, it lowers the specific thrust. Thus, in order to attain the necessary thrust, a considerable increase in the dimensions of the secondary flow (fan) is required. It is found to be possible, by acting opportunely on the compression ratio and the temperature, to improve efficiency without appreciably reducing specific thrust.

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I

CONTRIBUTION TO THE SELECTION OF THE PARAMETERS OF THE THERMODYNAMIC CYCLE IN DOUBLE FLOW TURBOJETS

Mario Albin and Massimo Feola*

1. Premise /1**

Double flow turbojets have been widely used in all the most important achievements of the past few years, including those relating to the largest and most recent civilian aircraft, such as the Boeing 747 (known also as the "jumbo jet"), the Lockheed L-1011 Tristar, the series of DC 10s, etc., and their use is also foreseen in projects under study for the near future.

Such a rapid diffusion is easily explained by calling to mind the fact that double flow minimizes the most serious drawback of jet propulsion in the range of the normal flight speeds of civilian subsonic aircraft, that is, as is well known, their relatively low propulsive efficiency.

The two-flow solution has, however, even been used in various military supersonic aircraft, among which we cite, for example, the American F11 and F14A, some versions of the French Mirage, /2
etc., and it is provided for even in this case in many projects now being developed. In regard to this, it is perhaps advisable to point out that even aircraft planned for supersonic flight must, for periods of time varying with the particular operative conditions, fly at subsonic speeds and must therefore be adapted to the use of double flow. It is clear, however, that passing to supersonic speeds calls for a considerable increase in thrust. This is generally obtained by means of normal afterburners.

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**Numbers in the margin indicate pagination in the foreign text.

At this point, it is necessary to make clear that these same double flow turbojets (which we will indicate from now on by the abbreviation DFTJ) have undergone a considerable evolution in the past few years, as evidenced by the related literature [1-3]. As a consequence, this has produced important changes in their principal functional and structural characteristics.

Until several years ago, in fact, the largest existing double flow engine, the Rolls Royce Conway RCo 43 (mounted in the "BAC Super VC 10"),provided a thrust of about 10,000 kgf with a bypass ratio between the primary and secondary flow capacities equal to 0.6. The same ratio was 0.7 in another classic engine, the Spey /3 RB 168-25R which, however, is different from the first, since it has only one BP compressor for both the flows. In reality, all the "first generation" DFTJs had analogous bypass ratio values.

However, in the most recent developments there has been a general and large increase in the secondary flow capacity, with the purpose of obtaining further increases in propulsive efficiency. For example, the Pratt-Whitney JT9D-3 engine, mounted in the aforementioned Boeing 747 and Douglas DC 10 (series 20), with a thrust of 19,000 kgf, has a bypass ratio of 5:1. Even greater values, i.e. of 6.2:1, with a thrust of 19,200 kgf have been attained in the General Electric CF6-6 engines installed in the Douglas DC 10 (series 10) [4].

It is therefore seen that if, on the one hand, as we will see, bypass ratio increase will improve the global thermopropulsive efficiency, on the other hand it will lower the thrust per unit mass capacity (specific thrust) by reducing the values of the speed of efflux. Obviously, this means that in order to attain the necessary thrusts, the mass flows must always be greater.

Thus, the considerable increase in the dimensions of the /4 secondary flow (fan) is explained; this has necessarily followed the increase in bypass ratio.

The considerable constructive and obstructive differences relative to two separated-flow[1] DFTJs with high and low bypass ratios are evident in the diagrams of Figs. 1 and 2. One sees, in fact, the considerable increase in the transverse dimensions that is partially compensated for by a reduction in the remaining part of the engine.

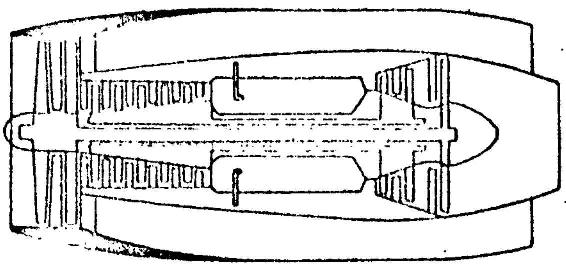

Fig. 1.

It is also useful to point out that increase in the transverse section increases the aerodynamic resistance. Because of this, a constantly greater thrust is required at equal flight speed. Since the total hourly consumption is established, as is known, by the product of specific consumption (kg per unit of thrust per hour) multiplied by the total thrust, a more or less noticeable attenuation of the advantage obtained from the small specific consumption follows from this.

The advantage of adopting double flow remains, however, very /5 clear, since it is obviously confirmed by recent studies.

Interesting analytic studies on double flow turbojets [7-8] have been done from the time of their first appearance, with the intention of examining the influence of various parameters on engine performance. In these, however, because of the particular theoretical form of the problem, the compression ratio of the fan

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[1]In the present study, we wished to limit the examination of the thermodynamic cycle to only separated-flow turbofans, avoiding extending the study also to mixed-flow turbofans, for which interesting studies have already appeared [5-6].

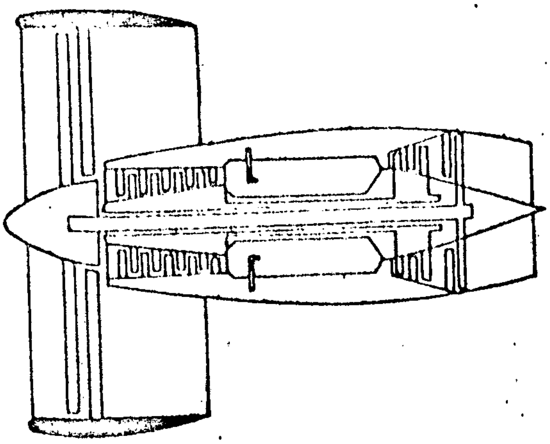

Fig. 2.

did not appear explicitly as a variable. Instead, it was indirectly linked -- for a percentage of work from the turbine that was determined from time to time -- to the quantitative relationship existing between the primary and secondary flows.

In regard to this, however, it is necessary to note that the moderate values of the secondary capacity in the first application had not yet made the fan take on the present distinct characteristics that distinguish it clearly from the other parts of the engine.

In another work on the topic [9], done recently by SNECMA (the well-known French aeronautics company), the calculations have instead been formulated starting from a fixed value (1.55) for the compression ratio of the fan considered as one stage. The expansion ratio of the nozzle has also been pre-established. By means of this, the bypass ratio was determined for each pair of values of the total compression ratio and the maximum cycle temperature.

In reality, it is easy to realize that because of the high /6
number of variables, the selection of the characteristic elements results in a planning situation that is much more delicate than in the case of a normal turbojet. For these reasons, it is considered useful in the present project to carry out an analytic investigation in which, in addition to considering variables such as the bypass ratio and fan compression, the principal phenomena that concern the real case are also adequately taken into account, in order to confer a satisfactory certitude on the results obtained.

In this regard, it is to be noted that the high total compression ratios in use today for the cycle (20 + 30) do not make more acceptable the hypothesis -- usually taken for granted in other studies -- that has to do with the constancy of the adiabatic-isentropic efficiency of the turbine and of the compressors.

Naturally, all of this has caused serious complications when making calculations, and this has made it necessary to resort to the use of a computer.

2. Form of a Study Model of the Thermodynamic Cycle /7

The elementary diagram of the DFTJ to which reference is made is represented in Fig. 3.[2] Figure 4 then shows the thermodynamic cycle in coordinates (T,s).

Referring to Fig. 4, one has:

0-1	autocompression in the dynamic intake
$1-2'_f$	compression of the fan
$2'_f-2'$	compression of the compressor
2'-3'	combustion
3'-4'	expansion in the turbine
4'-5'	expansion of the fluid (commonly known as a hot jet) in the primary nozzle.

The transformations that have been listed up to now refer, naturally, to the kilogram of fluid that evolves in the engine.

The x kilograms bypassed, on the other hand, go through the following transformations:

0-1	autocompression in the fan
1'-2'	compression in the fan

[2]The present treatment is limited, as has been said, to the solution of two separated flows, adopted in the largest DFTJ cited.

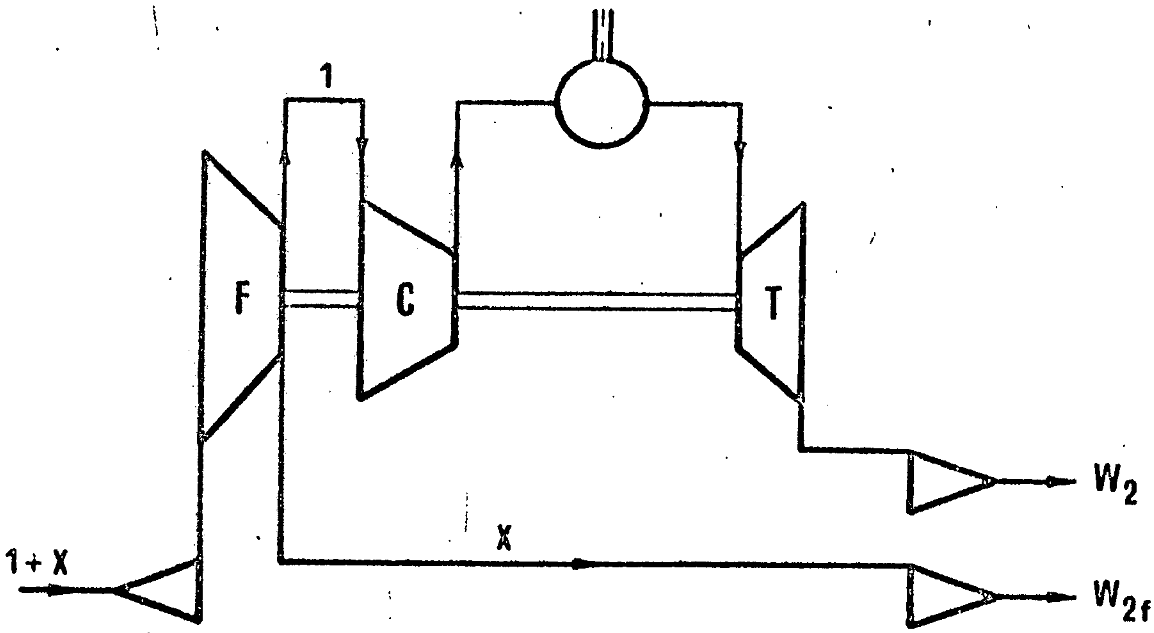

Fig. 3.

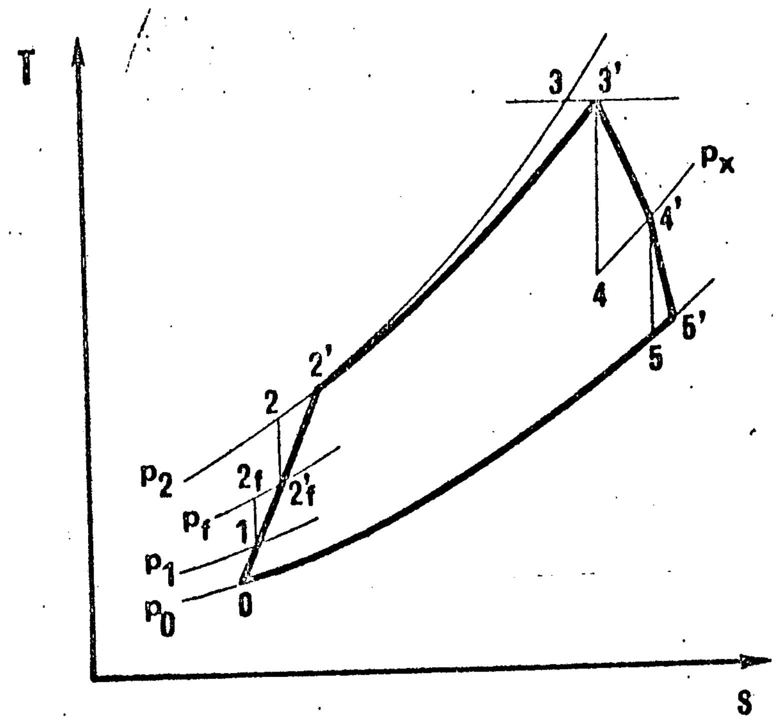

Fig. 4.

these last transformations being common to both the primary and /8
secondary flows,

expansion of the fluid (cold jet) -- not represented in Fig. 4 -- in the secondary nozzle.[3]

The DFTJ efficiencies that have to do with this case are those real thermal efficiencies η_r expressed by the ratio between the total kinetic power made available by the 1 + x kilograms of fluid and the heat provided by the unit of flow that passes through the engine being increased by the mechanical energy of the combustible[4]:

$$\eta_r = \frac{\left[\left(1+\frac{1}{\alpha}\right)\frac{w_2^2}{2} - \left(1-\frac{1}{\alpha}\right)\frac{u^2}{2}\right] + x\left(\frac{w_{2f}^2}{2} - \frac{u^2}{2}\right)}{c_p\left(1+\frac{1}{\alpha}\right) T_3 - c_p T_2' + \frac{1}{\alpha}\frac{u^2}{2}} \qquad (1)$$

The propulsive efficiency η_{prop} will be equal, on the other hand, to the ratio between the useful propulsion power and the kinetic power mentioned above:

$$\eta_{prop} = \frac{\left\{\left[\left(1+\frac{1}{\alpha}\right) w_2 - u\right] + x\left(w_{2f} - u\right)\right\}\cdot u}{\left[\frac{1}{2}\left(1+\frac{1}{\alpha}\right) w_2^2 - \left(1-\frac{1}{\alpha}\right)\frac{u^2}{2}\right] + x\left(\frac{w_{2f}^2}{2} - \frac{u^2}{2}\right)} \qquad (2)$$

Finally, the global thermopropulsive efficiency will be the product /9
of the two efficiencies first indicated, that is:

$$\eta_{TP} = \eta_r \cdot \eta_{prop} = \frac{\left\{\left[\left(1+\frac{1}{\alpha}\right) w_2 - u\right] + x\left(w_{2f} - u\right)\right\}\cdot u}{\left[c_p\left(1+\frac{1}{\alpha}\right) T_3 - c_p \cdot T_2'\right] + \frac{1}{\alpha}\frac{u^2}{2}} \qquad (3)$$

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[3]The expansion of the secondary flow, that begins in point 2', has not been reported in the figure inasmuch as entropies relative to a kilogram of fluid that describe the thermodynamic cycle are reported on the abscissa.

[4]Such a real thermal efficiency is also called "conversion" by some authors.

Another element of considerable interest is the specific thrust S_s (daN·s/kg) expressed by:

$$S_s = \frac{\left[\left(1+\frac{1}{\alpha}\right) w_2 - u\right] + x\left(w_{2f} - u\right)}{1 + \frac{1}{\alpha} + x} \tag{4}$$

It is useful at this point to call to mind that the particular functioning of the DFTJ appears clear if it considered that it essentially derives from a simple TJ in which a part of the useful enthalpic jump, instead of being entirely spent in acceleration of the primary jet, is utilized to compress the greater aliquot part x of the secondary flow. In such a way, in place of a single kilogram of fluid at the speed (certainly higher) of the simple TJ, there are 1 + x kilograms at a speed lower, both because of the smaller expansion of the primary jet (W_2) and because of the moderate fan compression ratio. /10

In other words, a greater quantity of fluid is obtained at lower speed. This fact is clearly very advantageous for the propulsive yield.

It should be noted, however, that the aliquot part of the useful enthalpic jump subtracted for the compression of the x kilograms is not entirely found again in kinetic energy of the secondary jet (apart from the losses in the propelling nozzle which will always be in both); it follows from this that the total kinetic energy made available, and therefore the real thermal efficiency, will always be noticeably less than the corresponding values of the simple TJ.[5]

[5]From what has been said, it is easy to deduce that when the adiabatic-isentropic efficiencies of the turbine and of the compressor are considered equal to one -- in an ideal hypothesis -- the real thermal efficiency of the DFTJ would be equal to that of a simple TJ [6].

On the whole, however, for the very different entity of the variations of partial efficiencies, the global thermopropulsive efficiency always improves, in general, to an extent that will be seen to be considerable.

The conditions to which reference was made have been those /11 that are usually "standard" for the civilian airline turbofan; i.e. a Mach number equal to 0.85 and a height of 11,000 meters with a corresponding flight speed of about 250 m/s (∿900 km/h). The values of the pressure and temperature at the entrance of dynamic intake were taken, for the previously mentioned height, from the tables of the "international atmosphere pattern" [10], and the results are below:

$$T_o = 216.65\ \mathrm{K}$$
$$p_o = 0.23078\ \mathrm{ata}$$

In order to calculate the conditions at the exit from the dynamic intake, it is assumed -- according to what has been suggested in the specialized work [11] -- that the fluid is completely slowed down, giving an adiabatic-isentropic η_{diff} equal to 0.92.

The total temperature T_1 will then be given from

$$T_1 = T_o \left[1 + \frac{k-1}{2} M^2 \right] \qquad (5)$$

in which

$[T_o]$ represents the temperature at the entrance of dynamic intake (assumed to be equal to the atmospheric temperature T_a);

M represents the Mach number at the entrance of the dynamic /12 intake, assumed to be equal to that corresponding to the flight speed at the height considered (i.e., $M = 0.85$ at 11,000 m);

K is the mean atomic ratio c_p/c_v of the air in the temperature range included between T_o and T_1.

By means of the aforementioned formula, the final temperature at the exit of the intake is shown to be equal to:

$$T_1 = 247.4 \text{ K}$$

The total pressure p_1 at the exit of the intake has been calculated by means of the formula:

$$p_1 = p_o \left[1 + \eta_{diff} \frac{k-1}{2} M^2\right]^{\frac{k}{k-1}} \tag{6}$$

in which:

p_o represents the pressure in the entrance section of the intake, assumed to be equal to the atmospheric pressure;

η_{diff} is the adiabatic-isentropic efficiency of the intake, assumed to be equal to 0.92.

Such a value was found to be equal to:

$$p_1 = 1.547 \; p_o$$

and has been assumed to be constant in successive calculations.

Thus, in regard to the thermodynamic properties of the fluids /13 evolving in the cycle (air and combustion products), the following procedures were followed:

a. With regard to air, we have limited ourselves to the single dependency of specific heats of temperature, adopting for this, as suggested by Kruschik [12], the following NACA formulas:

$$c_{pa} = c_{pa}(T) = 0{,}2445 - 3{,}9708 \cdot 10^{-5} T + 8{,}9359 \cdot 10^{-8} \cdot T^2 \tag{7}$$

valid for a maximum temperature T equal to 633 K;

$$c_{p_a} = c_{p_A}(T) = 0{,}2413 + 1{,}088 \cdot 10^{-3}\sqrt{1{,}8T-976} \quad (8)$$

for temperatures higher than 633 K.

b. With regard to the combustion products, reference was made to the General Electric tables [13] of combustion products that are a mixture of air and kerosene. As is known, these tables report, for fixed pressure values, the specific heat of combustion products as a function of the temperature and of the original air-combustible ratio.

Referring to the values of specific heat reported in the aforementioned tables (as a function of the temperature and of the air-combustible ratio), the following function was determined with continuity by means of "interpolation in two variables": /14

$$c_{p_g} = c_{p_g}(T_3, \alpha)$$

This has permitted the determination, with sufficient precision, of the thermodynamic properties of combustion gases.

Given this, the fundamental relationship of equilibrium between the work of the turbine and the sum of the work of fan and compressor becomes, with reference to the notations of the cycle in Fig. 4:

$$\frac{(1+x)}{\eta_{f\,ad.\,is.}} c_{p_a} T_1 \left[\beta_f^{\frac{k_a-1}{k_a}} - 1\right] + \frac{1}{\eta_{c_{ad\,is.}}} c_{p_a} T'_{2f} \left[\beta_c^{\frac{k_a-1}{k_a}} - 1\right] = \eta_{T_{ad.\,is.}} (1+1/\alpha) c_{p_g} T_3 \left[1 - 1/\beta_x^{\frac{k_g-1}{k}}\right] \quad (9)$$

In this equation:

$\beta_f = p_f/p_1$ the fan compression ratio

$\beta_c = p_2/p_f$ the ratio of compression in the compressor

$\beta_x = p_2/p_x$ the unknown expansion ratio in the turbine

$\eta_{f\,ad.is.}$, $\eta_{c\,ad.is.}$, $\eta_{T\,ad.is.}$ are, respectively, the adiabatic-isentropic efficiencies of the fan, compressor, and turbine /15

c_{p_a}, k_a represent, respectively, the mean specific heat as well as the mean ratio c_p/c_v of the air during the phase of compression in the fan and in the compressor, functions, as it was assumed, of a single temperature

c_{p_g}, k_g represent the mean specific heat and the mean combustion ratios c_p/c_v of combustion gases during the expansion phase in the turbine; as has been said, functions of the air-combustible ratio and of the temperature T_3 at entrance into the turbine

T_1, T'_{2f} are the real temperatures of the beginning of compression in the fan and compressor, respectively

T_3 is the intake temperature in the turbine

x is the bypass ratio of the fan

α is the air-combustible ratio in the burner.

In regard to the adiabatic-isentropic efficiencies of the fan, compressor, and turbine, it was desirable to take into account in the calculations their variability with respect to the compression ratio. It is known, in fact, that for the so-called phenomena of "recovery" and "counter-recovery," the efficiency of expansion /16 increases as the ratio of expansion increases, while that of compression, on the contrary, decreases; and given that, in the case studied, the variations of the expansion and compression ratios are rather large, it was thought that to neglect such phenomena would have excessively influenced the certainty of the results obtained.

Meanwhile, conforming to the related criterion suggested in current literature [12, 14], the polytropic efficiencies of the fan, compressor and turbine have been considered constant (and equal to 0.88); while the respective adiabatic-isentropic efficiencies were variable according to the following formulas:

$$\eta^{f}_{ad.is.} = \frac{\beta_f^{\frac{ka-1}{ka}} - 1}{\beta^{\frac{ka-1}{ka} \frac{1}{\eta_c pol}} - 1} \tag{10}$$

$$\eta^{c}_{ad.is.} = \frac{\beta_c^{\frac{ka-1}{ka}} - 1}{\beta_c^{\frac{ka-1}{ka} \frac{1}{\eta_c pol}} - 1} \tag{11}$$

$$\eta^{T}_{ad.is.} = \frac{1 - 1 \Big/ \beta_x^{\frac{kg-1}{kg} \cdot \eta_T^{*} pol}}{1 - 1 \Big/ \beta_x^{\frac{kg-1}{kg}}} \tag{12}$$

For the turbines, moreover, reference was made to the corrected /1 polytropic efficiency $\eta_T^* pol$ rather than to the real one ($\eta_T pol$ = = 0.88) in order to take into account -- according to the criterion generally adopted [11, 12] -- the load losses from the compressor to the turbine due to the presence of the combustion chamber and the feed lines.

As suggested by [15], considering that such losses are equal to 3%, the following formula was adopted:

$$\eta_T^{*} pol = \eta_T pol \left[1 + \frac{\ln(1-\varepsilon)}{\ln(p_2/p_x)} \right] \tag{13}$$

In such a way, considering this the "fictitious" polytropic efficiency expressed by (19), one can assume a value for the pressure at the entrance ot the turbine that is constant and equal to that at the exit of the compressor p_2.

Starting from (9), the determination of the unknown expansion ratio of the turbine p_2/p_x has made it possible to give a value to the expansion ratio in the primary nozzle p_x/p_o. Based on this, the real temperature of entry into the nozzle, T_4, has been obtained and thus the actual enthalpic jump of the primary jet:

$$\Delta h_{ug.prim.} = c_{p_g} \cdot T_4' \left[1 - 1 \Big/ \beta_x^{\frac{k_g - 1}{k_g}} \right] \eta_{ua.} \qquad (14)$$

in which $\eta_{uG.}$, as suggested by [11], has been considered equal to 0.985. /18

The final speed of the primary flow has been obtained by means of the formula:

$$w_2 = \sqrt{2 \left(h_4' - h_5' \right)} \qquad (15)$$

while that of the secondary has been obtained analogously with

$$w_{2f} = \sqrt{2 \Delta h_f} \qquad (16)$$

Δh_f being equal to:

$$\Delta h_f = c_{p_a} T_{2f}' \left[1 - 1 \Big/ \left(\frac{p_f}{p_o} \right)^{\frac{k_a - 1}{k_a}} \right] \cdot \eta_{ua.}' \qquad (17)$$

The final air temperature has been calculated taking into account the successive compressions in the intake, fan, and compressor. In addition, for each value of the maximum temperature /19
of the cycle T_3, the ratio α air/combustible has been calculated based on the formula:

$$\frac{Hi}{1+\alpha} = c_{p_g}\left(T_3\,;\alpha\right) - c_{p_a} \cdot T_2' \tag{18}$$

This formula, fixed for kerosene H_1 = 10,300 kcal/kg, has been solved with respect to α with a numerical program of interpolation with two variables.

3. Methods of Performing the Calculations /20

If the above premises are granted, the thermopropulsive efficiency and the specific thrust have been calculated by means of the preceding formulas (3) and (4) by varying the following factors parametrically and systematically:

a. The global and inclusive ratio of cycle compression (in the fan and compressor):

$$\beta = p_2/p_1 = p_2/p_f \cdot p_f/p_1 = \beta_f \cdot \beta_c$$

made to vary parametrically (with step 4) between the value limits 10 and 30;

b. The ratio β_f of compression in the fan:

$$\beta_f = p_f/p_1$$

made to vary parametrically between the value limits 1 and 4 (step 0, 2);

c. The temperature T_3 of turbine induction, made to vary systematically (step 50) between the value limits 973 K (700°C) and 1573 K (1300°C);

d. The x bypass ratio of the fan between the minimum value x = 0 (corresponding to the case of a "pure" turbojet) and a maximum value of 10 with a unit step.

The calculations have been done with the help of the Bendix computer of the computer center of the Faculty of Engineering at Naples[6] for a number of very difficult cases in which the ample interval of variation taken for granted for each of the variables first indicated is taken into account. However, the real solutions -- those that are physically compatible with the condition of equality for the work of the turbine and of the compressors -- have given practical results that are much less than those theoretically predictable. /21

The data furnished by the computer, reported on graphs, give an interesting analysis of the various parameters on the performance of the turbojet.

We refer, above all, to the two most characteristic elements, i.e. the ratios of compression and of bypass of the fan. We select for this first phase of examination 1373 K (1100°C) as the value of the [unknown term: perhaps typographical error for 'maximum'] temperature, and the value 26 as that of the compression ratio, given that these are rather common in the present-day DFTJ. Later we will also consider the influence of the other elements of the cycle.

For a certain bypass ratio, for example 8, Fig. 5 shows the slope of various efficiencies as a function of β_f. With this last, in fact, the work of expansion increases and hence the speed of the secondary jet also increases. The total work of compression, however, increases with β_f, in greater measure and equal more exactly to (1 + x) times the work per unit of fluid. /22

[6] The authors express their most cordial appreciation to Professor Lucio Sansone, of the above-mentioned center, for the valuable collaboration which he courteously provided.

Meanwhile, the useful jump of the primary nozzle and its speed will diminish as a rule much more rapidly.

This explains, therefore, the considerable increase of the propulsive efficiency with β_f. However, this reaches a maximum when the two speeds equal each other. A further increase of β_f, in fact, leads to a worsening due to the increase in the speed of a secondary jet, and the propulsive efficiency begins to decrease.

The practical global efficiency follows the slope of the propulsive efficiency and has about the same value β_f for its maximum. Therefore, this results in the optimal compression ratio to be assigned for the given x.

It is interesting to note that the real thermal efficiency, instead of always diminishing as β_f increases, as was to be expected, exhibits a slightly centralized minimum.

An explanation is found for this fact in the influence of the distribution of work between the fan and the second compressor on the variation of adiabatic efficiency of the two machines.

The graphs are relative to a specified mass flow, and thus the maximum of the specific thrust will also coincide with the maximum efficiency.

Figure 6 shows the curves of global thermopropulsive effi- /23 ciency and of specific thrust for different values of x. One notes that as x grows larger, the thermopropulsive efficiency increases, but the maximum moves around smaller values of β_f. In fact, while the speed of the secondary jet grows with β_f, always with the same law, that of the primary jet diminishes more rapidly, because greater work is taken from the expansion, and first of all the conditions of speed proximity are reached.

It is then explained how the increase in bypass ratio, and therefore in radial fan dimensions, would be accompanied by a diminution of the number of stages, often reduced to only one.

If instead β_f is maintained constant, increasing the bypass ratio, one has the slope of Fig. 7. In this case, the speed of the secondary jet is invariable, while that of the primary jet, as usual, decreases with x because of the greater work of compression. Also in this case, the propulsive efficiency presents a maximum in values near the speed of the two jets. Since the thermal efficiency, also predictable, diminishes slightly, one has a maximum of global efficiency which corresponds approximately to that of the thermopropulsive efficiency.

In this case, there is also an optimal value of x for each β_f, just as before there was an optimal β_f for a specified bypass ratio. /24

In Fig. 8, the curves of global efficiency for different values of β_f have been brought together. From these, it is noted that, in accordance with the graph in Fig. 6, the optimal value of x increases as the compression ratio decreases.

A very interesting consideration emerges, however, from all the graphs examined up to now. Supposing, as has been done, that the compression ratio and the maximum temperature of the cycle are considered constant; the global efficiency increases obtained as the bypass ratio gets larger are rather moderate, while the specific thrust decreases considerably.

It is seen from Fig. 8, in fact, that in passing, e.g., from $x = 4$ and $\beta_f = 2$ to $x = 7$ and $\beta_f = 1.6$, the global efficiency increases from 0.31 to 0.32; in contrast, the specific thrust decreases from 19 to about 13. In essence, the capacity of the secondary jet is practically doubled when there is only a 1% improvement in efficiency.

It is evident, therefore, that the simple variation of x and β_f is not enough to assure conditions of actual suitability. These, instead, should be looked for with a suitable selection of the /25 other parameters of the turbojet cycle.

In order to examine also this other aspect of the problem, very important to the goals of the project, the study has been extended by making large variations in $\beta(p_2/p_1)$ of the cycle and in the maximum temperature. Graphs 9 and 10 are significant examples in which, for the given β_f e x, T_3 is made to vary for different values $\beta(p_2/p_1)$ of the cycle.

The thermal efficiency increases with T_3 while the propulsive efficiency decreases because the speed of the primary jet increases. There is, therefore, as in the simple turbojets, an optimal temperature of maximum thermopropulsive efficiency.

The specific thrust, as is natural, increases with T_3, but, as has been noted, in a manner that is always less accentuated as the bypass ratio increases. In fact, the increase of T_3 only influences the primary jet, and that loses importance as the capacity of the secondary jet increases.

By bringing together the various maximum points of efficiency (for different $\beta(p_2/p_1)$ cycle [sic]), it is seen that such a line takes on a gradually increasing slope. This shows that the increase of T_3 should be followed by a corresponding increase in the total compression ratio. But the influence of this last parameter emerges in a more evident manner from graphs 11, 12, and 13, in which $\beta(p_2/p_1)$ is reported on the abscissas.

For temperatures that are not relatively very high (for /26
example, 1173 K), the global efficiency shows a maximum, while the specific thrust decreases to a rather considerable extent. For greater temperatures, the efficiency maximum has values of the β ratio of the cycle that are higher than the limit predicted in the calculations (30) while the specific thrust decreases in a manner less and less noticeable. (It is noted, for example, that for 1473 K, it remains almost constant.)

It is therefore possible, by acting opportunely on the compression ratio and on the temperature, to improve the efficiency without reducing the specific thrust in an appreciable manner. The interest in these conclusions is made still more evident by calling to mind that in the double flow turbojets the increase in efficiency has been, up to now, essentially obtained with increases of the bypass ratio and therefore with considerable sacrifice of the specific thrust.

As an example, we observe from graphs 12 and 13 that passing from a ratio $\beta = 21$ and $T_3 = 1273$ K to $\beta(p_2/p_1) = 30$ and $T_3 =$ $= 1373$ K, it is possible to maintain approximately the same efficiency ($\eta_{TP} = 0.285$). However, the bypass ratio is reduced from 7 to 5 and this produces a variation in the specific thrust from 11.6 to 16, with an increase that is anything but negligible.

In other words, if one thinks of the considerable increase /27
in fan dimensions, characteristic of most recent accomplishments, it seems clear that this report could lead to a reversal of this tendency. The transverse dimensions of the fan could be reduced as well as the "winged gondolas," at a parity with total thrust, and with an advantage, therefore, on the total resistance for the advancement of the aircraft. It is evident that, by maintaining the same fan dimensions, it would be possible to have greater total thrusts.

At this point, it is best to make clear that the increase in the compression ratio has been for some time a current practice in the large double flow turbojets. For this reason, it has been necessary to resort to two or even three axles in order to assign each one the most suitable number of turns. It is still necessary to point out that the values from 26 to 30 reached up to now appear, in the light of the results of this study, still insufficient for the best utilization of the maximum temperatures that can be reached today (1100 to 1200°C and more) with the use of the special materials available and with an adequate cooling of the turbine blades.

On the other hand, one knows the difficulties that are met by the axial compressor beyond certain values of the compression ratio that require a very high number of states. The problem /28 would certainly be able to be simplified by a further and substantial progress in transsonic and supersonic compressors in order, as is known, to obtain a higher compression ratio per stage, but the practical applications are still rather limited.

In conclusion, we would like to observe that the presumable further evolution of the double flow turbojet will probably also be linked to further progress in the various engine components, and that will have an important influence on the selection criteria to be adopted -- criteria that have been projected and illustrated in this study.

List of Symbols /29

η_{prop}	propulsive efficiency
η_r	real thermal efficiency
η_{TP}	global or thermopropulsive efficiency
η_{diff}	adiabatic-isentropic efficiency of the dynamic intake

$\eta_{uG.}$	adiabatic-isentropic efficiency of the primary (or hot) nozzle
$\eta'_{uG.}$	adiabatic-isentropic efficiency of the secondary (or cold) nozzle
$\eta_{f_{ad.is.}}$	adiabatic-isentropic efficiency of the fan
$\eta_{c_{ad.is}}$	adiabatic-isentropic efficiency of the compressor
$\eta_{T_{ad.is.}}$	adiabatic-isentropic efficiency of the turbine
η^{pol}_{f}	polytropic efficiency of the fan
η^{pol}_{c}	polytropic efficiency of the compressor
η^{pol}_{T}	polytropic efficiency of the turbine
η^{*pol}_{T}	corrected polytropic efficiency of the turbine
S_s	specific thrust (N/kg)
T_o	atmospheric temperature of the height considered (K)
T_1	total temperature at the exit from the intake (K)
T_{2f}	adiabatic-isentropic temperature of fan compression (K)
T'_{2f}	real temperature of the adiabatic compression of the fan (K)
T_2	adiabatic-isentropic temperature of compression of the compressor
T'_2	real temperature of compression of the compressor (K)
T_3, T'_3	entry temperature of combustion products in the turbine (K)
T_4	adiabatic-isentropic temperature of expansion in the turbine (K) /30
T'_4	real temperature of adiabatic expansion in the turbine (K)
T_5	adiabatic-isentropic temperature of expansion in the primary nozzle (K)
T'_5	real temperature of final adiabatic expansion in the primary nozzle (K)
p_o	atmospheric pressure at the height considered (ata)
p_1	total pressure at the exit from the intake
p_f	total pressure of the turn of the fan
p_2	total pressure of the turn of the compressor
p_x	total pressure at the exit from the turbine
β_f	compression ratio (p_f/p_1) of the fan

β_c	compression ratio (p_f/p_1) of the compressor
β_x	expansion ratio (p_2/p_x) in turbine
β	total compression ratio of the cycle (p_2/p_1)
u	flight speed (m/s)
Mn	Mach number
w_2	relative speed of exit from the primary nozzle (m/z)
w_{2f}	relative speed of exit from the secondary nozzle (m/s)
x	bypass ratio of the fan
c_{p_a}	specific heat at constant air pressure (kcal/kg°K)
k_a	atomic ratio cp/cv of the air
c_{p_g}	specific heat at constant pressure of the combustion products (kg/kg°K)
k_g	atomic ratio (cp/cv) of the products of combustion /31
α	ratio of air/combustible
Hi	calorific power of the combustible -- kerosene (Kcal/kg)
Δh_f	real enthalpic jump in the secondary nozzle
$\Delta h_{4'5'}$	real enthalpic jump in the primary nozzle

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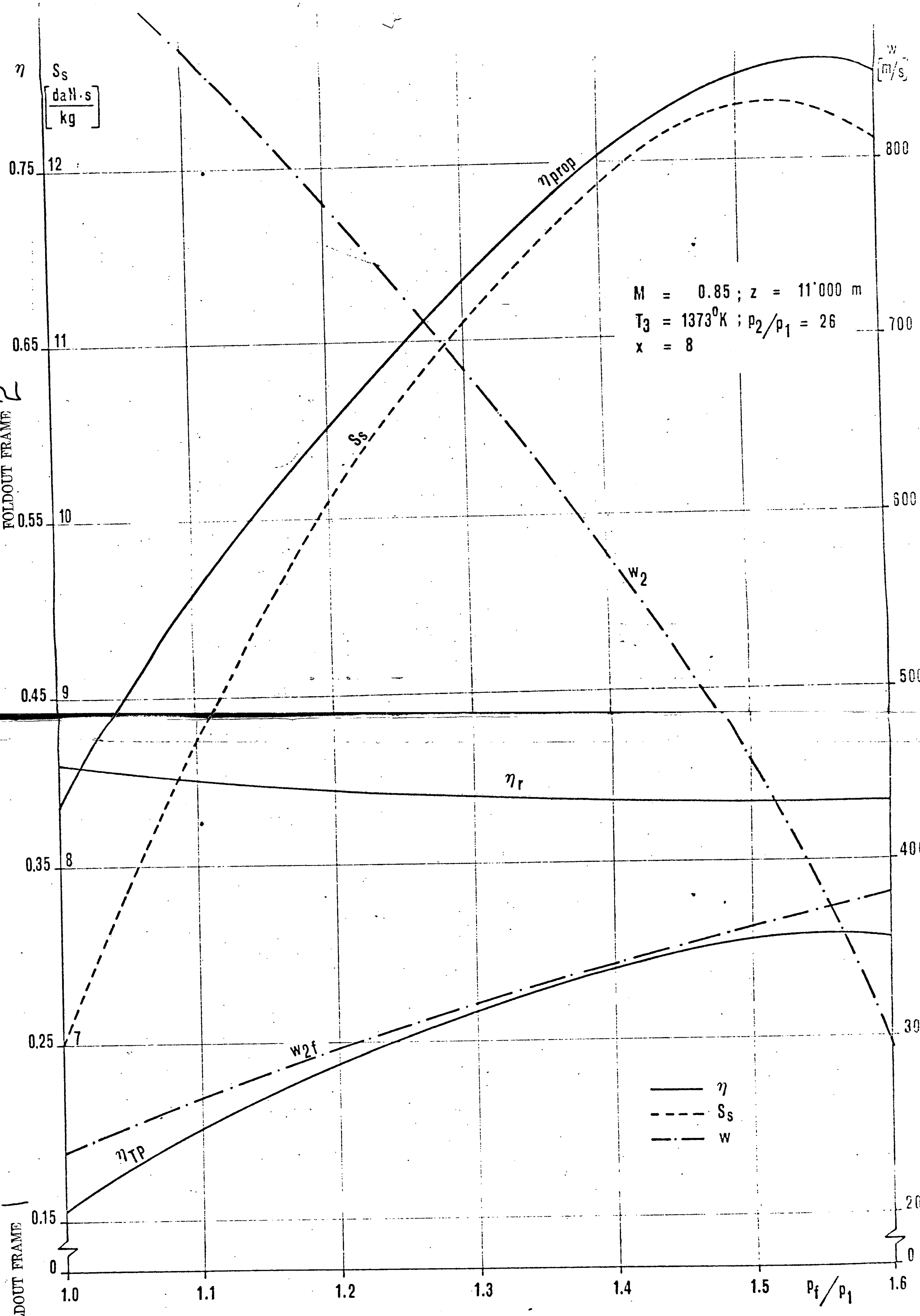


FOLDOUT FRAME 2

FOLDOUT FRAME

FIG. 5

FOLDOUT FRAME 2

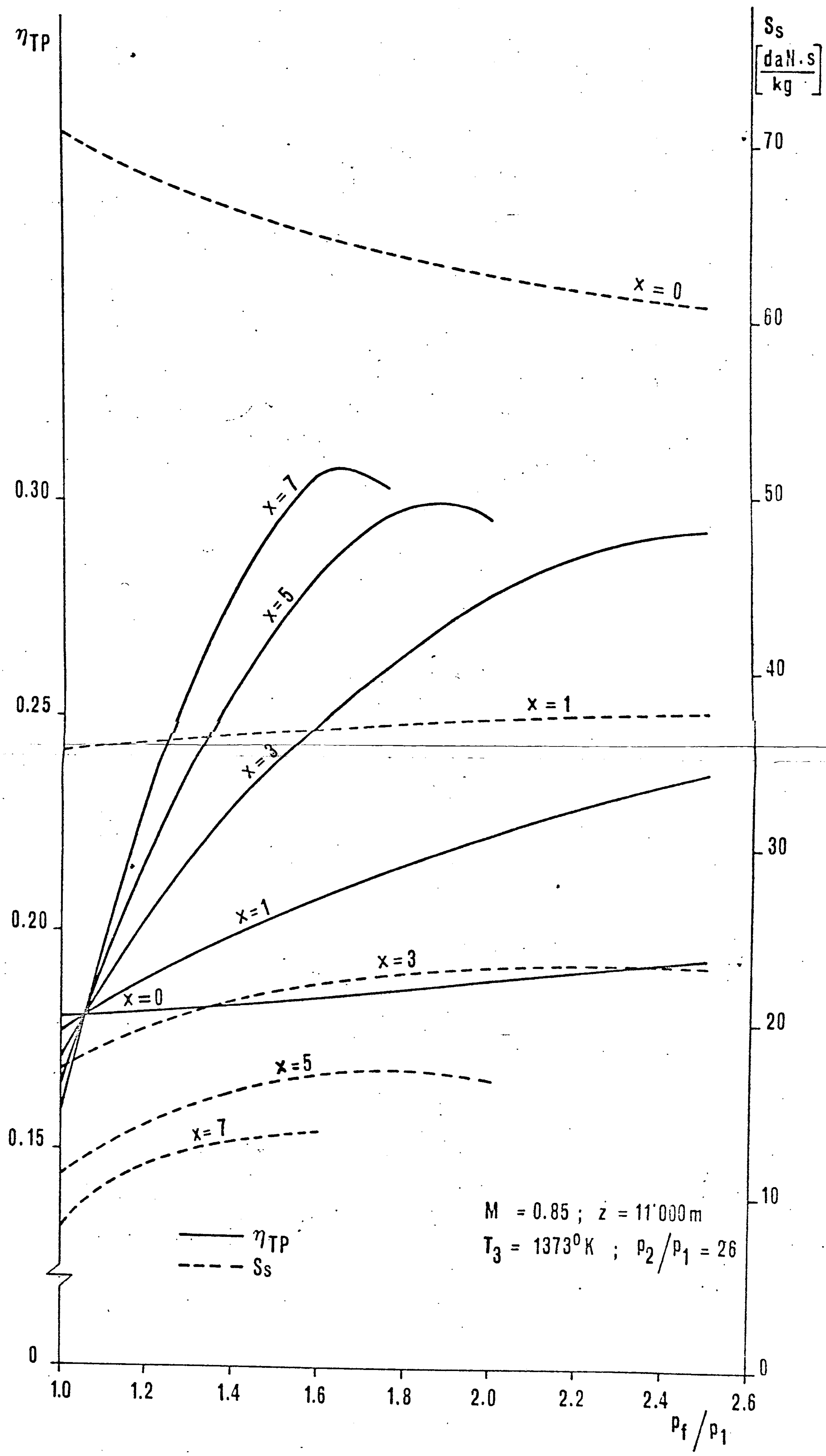
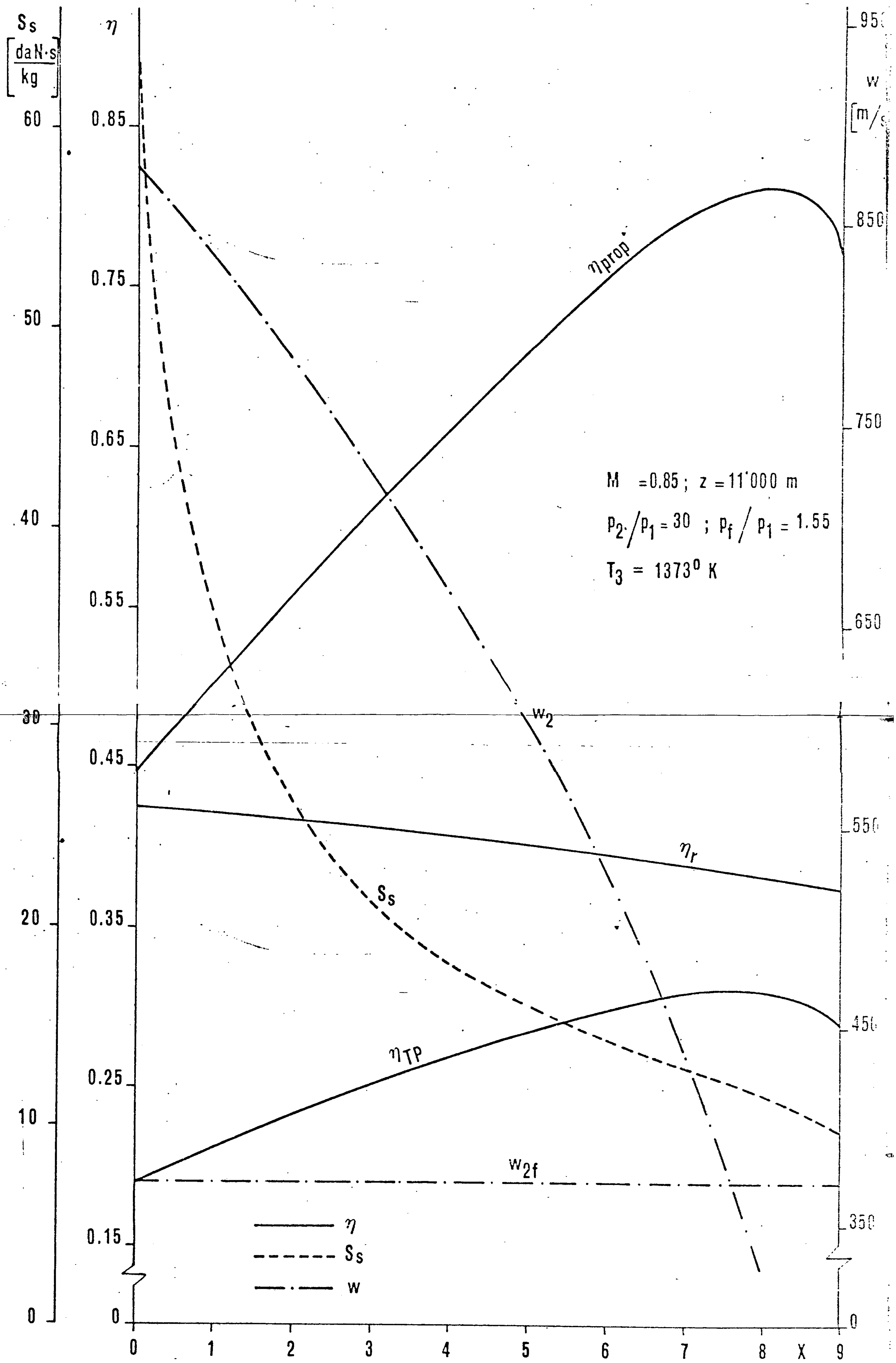


FIG. 6

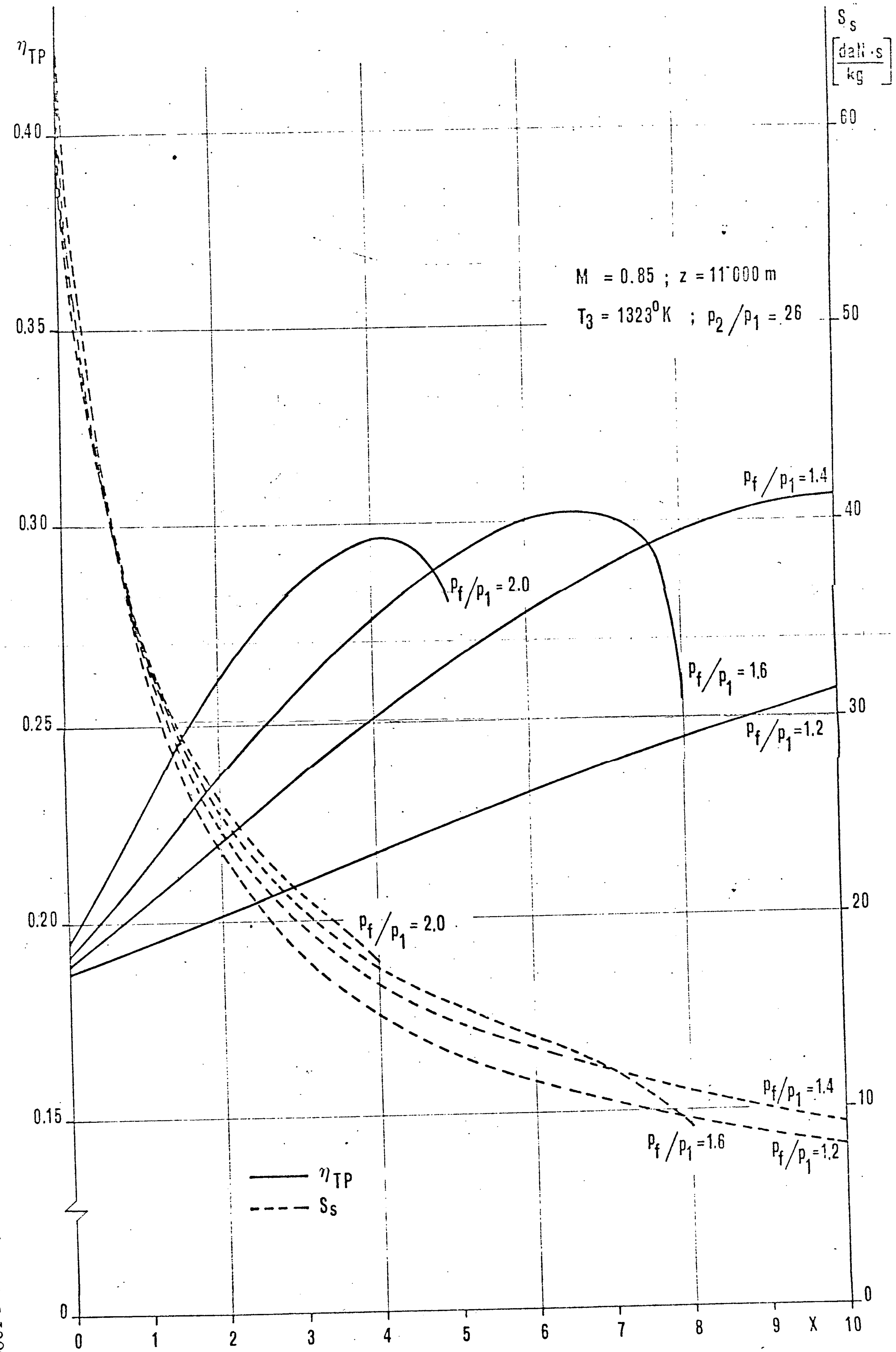
FOLDOUT FRAME 1



Ss [daN·s/kg]
η
w [m/s]
60
50
40
30
20
10
0
0.85
0.75
0.65
0.55
0.45
0.35
0.25
0.15
0
950
850
750
650
550
450
350
0
η prop
w2
ηr
Ss
ηTP
w2f
M = 0.85 ; z = 11'000 m
p2/p1 = 30 ; pf/p1 = 1.55
T3 = 1373° K
η
Ss
w
0
1
2
3
4
5
6
7
8
X
9

Fig. 7 28

FOLDOUT FRAME 2



FOLDOUT FRAME 1

3,9 8

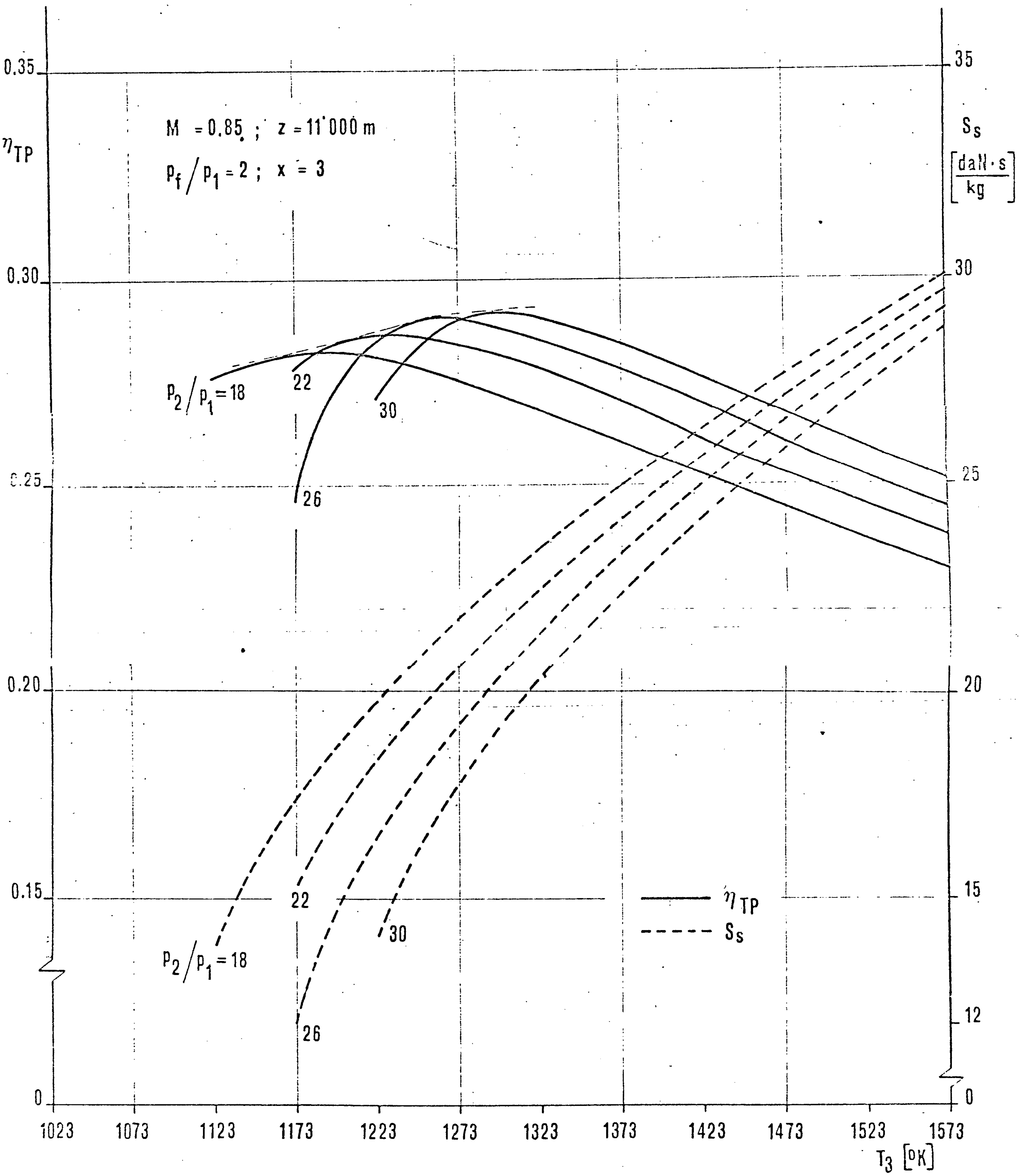


FIG. 9

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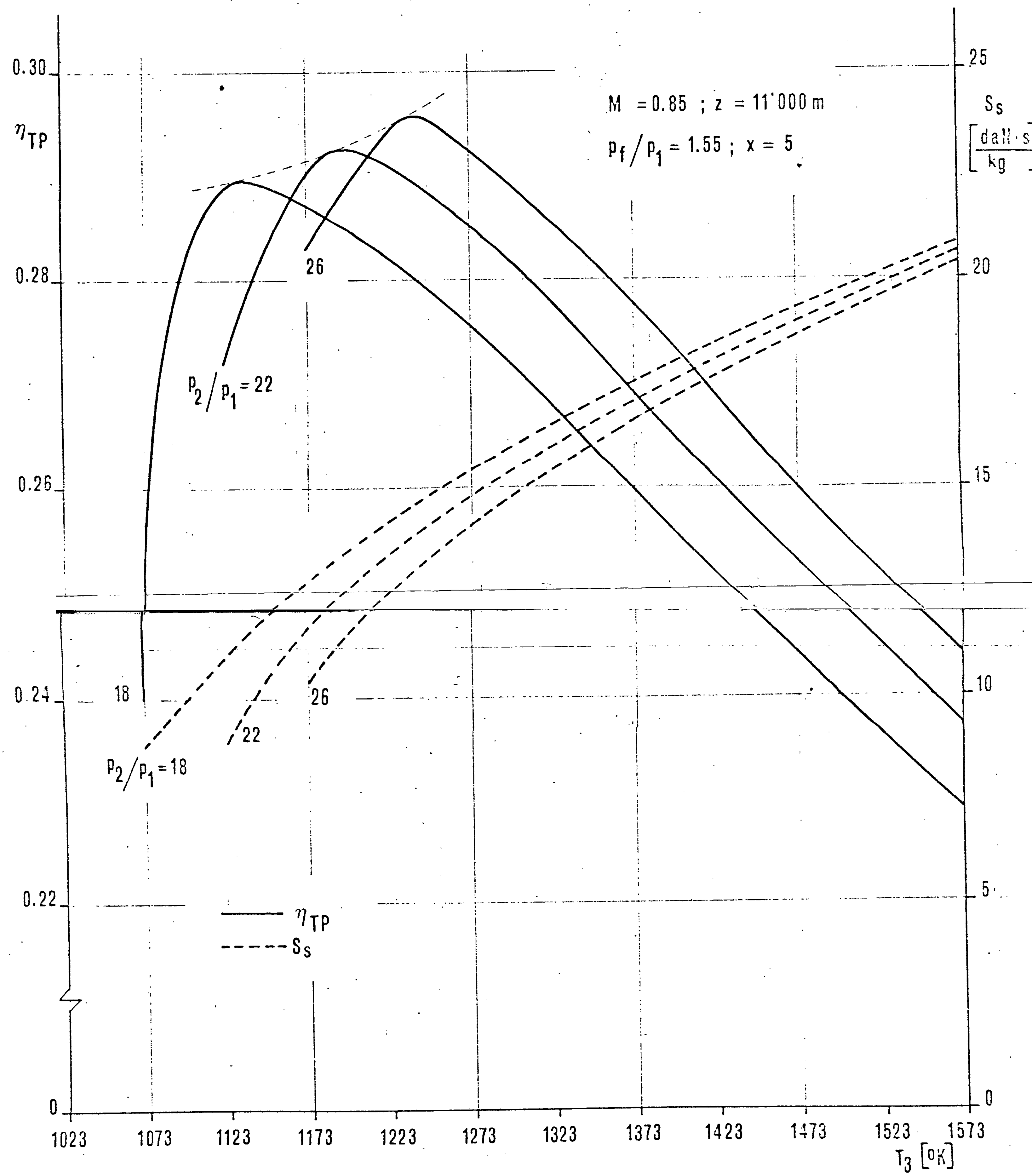

FIG. 10

FOLDOUT FRAME 2

FOLDOUT FRAME 1

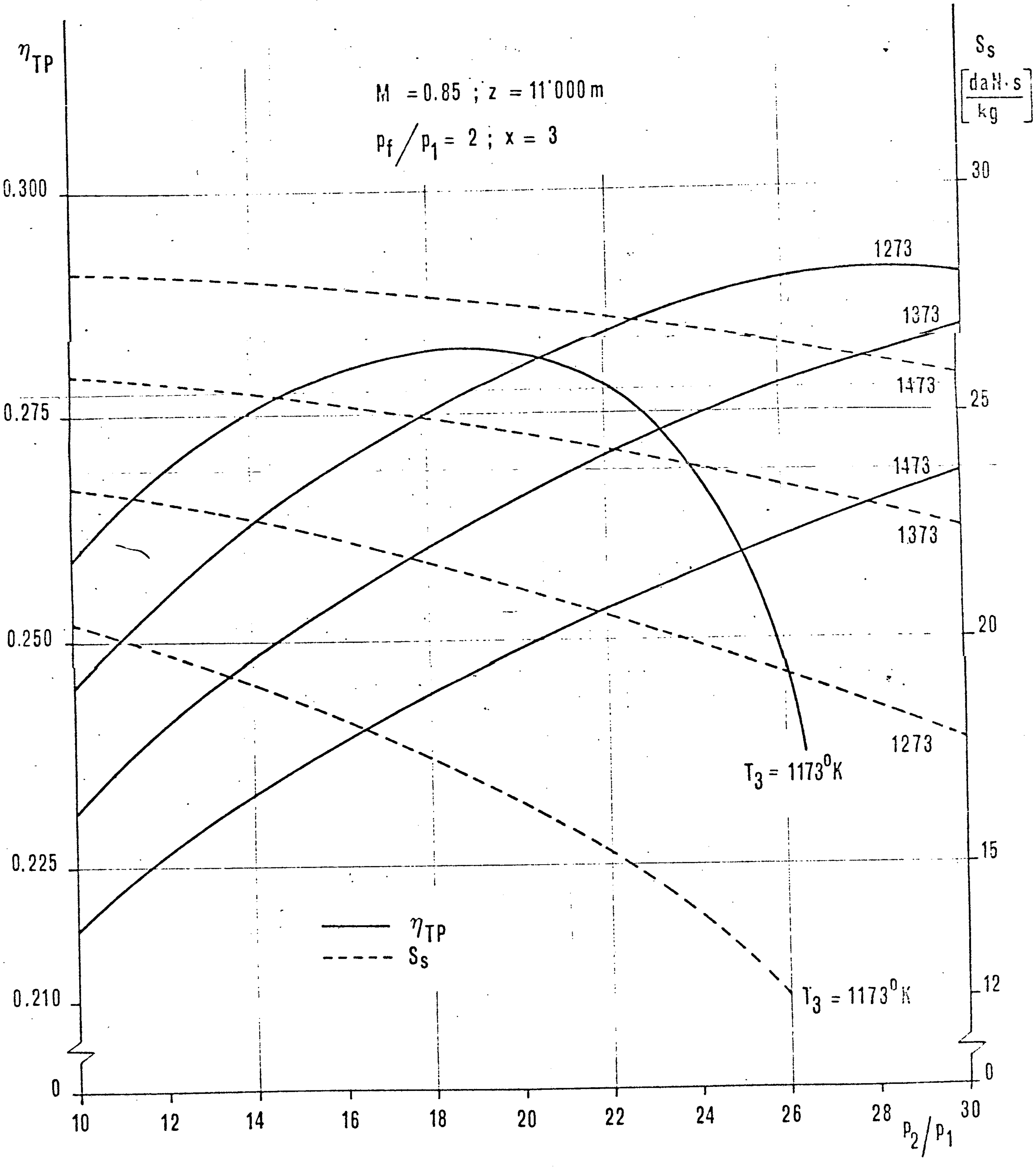

FIG. 11

FOLDOUT FRAME 2

FOLDOUT FRAME 1

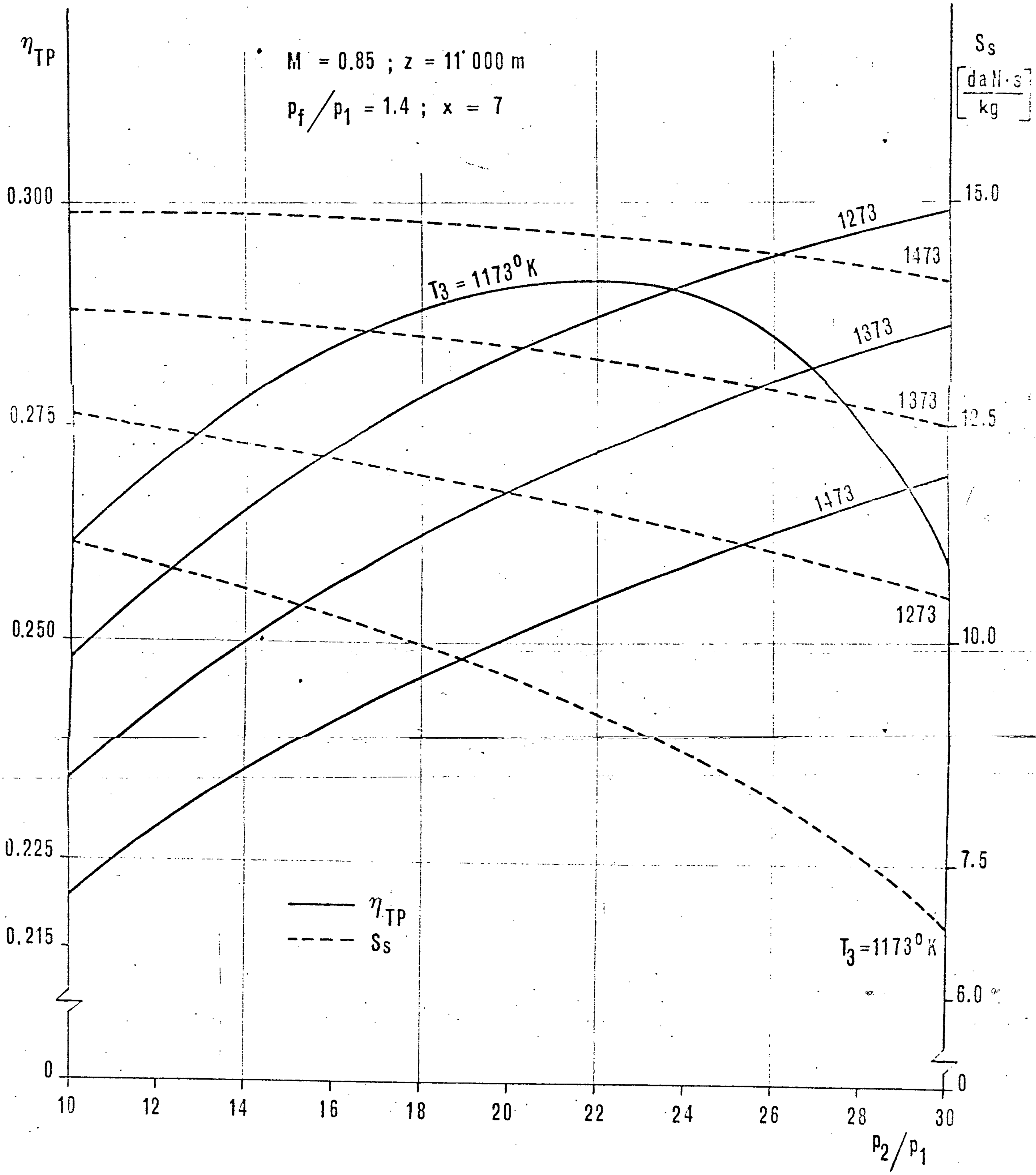

FIG. 12

FOLDOUT FRAME 2

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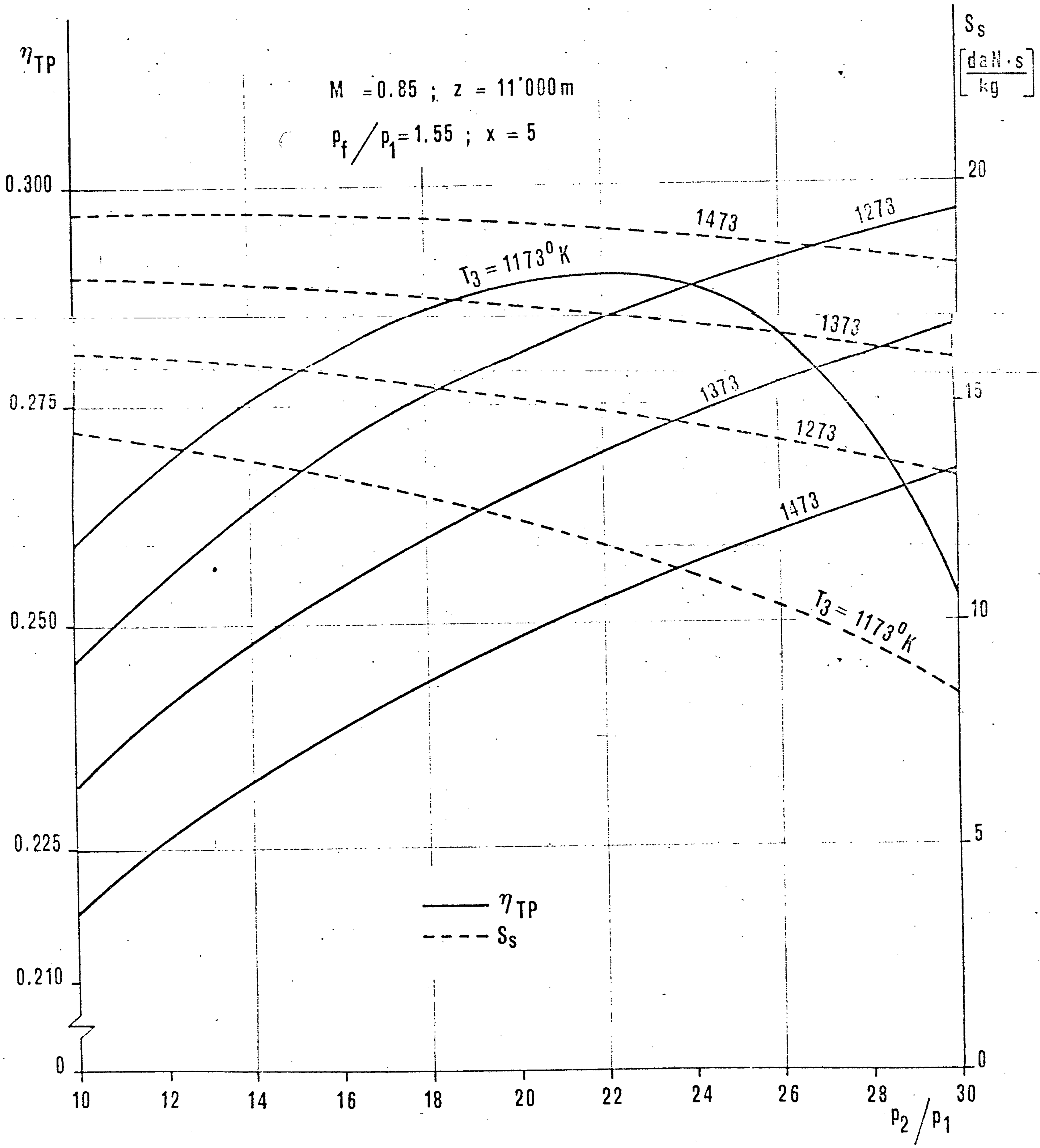

Fig. 13

FOLDOUT FRAME